WORN TOOL FORCES BASED ON PLOUGHING STRESSES

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ABSTRACT

Recent work in modeling of the ploughing mechanism in basic metal machining may provide a means of estimating the additional forces to be expected when cutting with a worn tool. The results predict the rubbing stresses due to the finite radius of an unworn tool edge. Since an unworn tool can be thought of as a worn tool with a wear land width VB = 0, these stresses can make up part of a strategy for predicting the additional forces incurred by a worn tool. This paper develops a wear model by proposing a technique for utilizing the stresses predicted by the ploughing model to estimate the stresses on the flank based on both elastic contact and plastic flow at the flank. Orthogonal cutting experiments with worn tools were performed to test the approach. Predictions for these tests, as well as for previously published results, are presented and show great promise for achieving a reliable wear-force prediction strategy.

INTRODUCTION

An understanding of the effects of tool wear has long been sought in machining research. Such knowledge would facilitate making process decisions related to tool wear criterion selection, tool wear monitoring, fixturing for a wearing tool, and predicting workpiece quality and machine stability under worn tool conditions. Most of these decisions depend on how accurately forces can be predicted for a worn tool. Researchers have developed strategies for predicting the change in cutting forces based on a knowledge of the wear geometry, though the only approach widely adopted by industry is to run expensive and time-consuming wear tests for the purpose of fitting an empirical model relating forces to the level of wear.

Some assumptions of wear-force models are common to virtually all the research to date. An approximately constant land width VB is assumed along the cutting edge. Forces on the wear land are proportional to the normal σ_W and shear τ_W stresses acting there according to

$$F_{tw} = \int_0^{VB} \sigma_w(x) \cdot dx \cdot w \quad , \qquad (1)$$
$$F_{cw} = \int_0^{VB} \tau_w(x) \cdot dx \cdot w$$

where F_{cw} and F_{tw} are the cutting and thrust forces, respectively, acting on the flank. The stresses depend on the nature of the contact between the flank and the workpiece, and debate has developed over whether that

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contact involves significant plastic flow of the workpiece or rather is elastic in nature with plastic flow occurring only at the tips of the asperities in contact. Some have indicated that both regimes may be present.

Evidence of significant plastic flow on the flank has been presented in [1-3]. These studies found that the size of the wear land does not significantly alter the shearing mechanism in the primary shear zone. Each used a grind-on method for producing a wear land on the tool and concluded that the average shear stress at the flank land is approximately constant and equal to the shear yield stress at the flank k_f, which may be different than the shear zone yield stress due to the different temperature, strain, and strain-rate which exist. In [1] and [2], and later adapted in [4], the average normal pressure on the flank is assumed to be equal to the effective yield strength of the material. In [3], it was found that average normal stress increases rapidly as VB increases. The grind-on procedure used by these studies was criticized by [5] who found different results using a grinding method followed by an additional procedure of natural wear. A slip-line field for force prediction of a worn tool with a chip breaker was proposed in [6], in which the frictional stress on the rake and flank faces are both assumed to be proportional to the material shear flow stress k. The normal stresses on the rake and flank faces depend on the slip-lines and are computed from the governing equilibrium equations. No experimental data, however, accompanied their simulations.

Other researchers [7-9] have argued that the contact at the worn flank surface is elastic in nature. Chen and Pun [5] experimented with worn tool forces and found the observed forces to be much smaller than those corresponding to plastic yield. Both [7] and [9], along with Ber and Friedman [10], postulate a parabolic distribution of normal stress on the flank consistent with the normal pressure distribution given by Johnson [11] for the elastic contact of a flat, rigid punch indenting a half-space.

This paper proposes a wear-force model capable of estimating the effects of wear on cutting forces based only on results collected from a small number of sharp tool tests. The technique combines recently published results [12] from a ploughing model to estimate the stresses on a flank land based on both elastic contact and plastic flow at the flank.

PROPOSED MODEL

In the proposed model, the total forces are due to shearing, normal to (F_n) and parallel to (F_s) the shear plane, and flank contact, normal to (F_{tw}) and parallel to (F_{cw}) the flank wear land. As shown in Figure 1, the cutting edge radius is assumed to be small compared to the wear land. Although the stresses at the tool tip are important for the analysis, the ploughing contribution to the total forces is small because of the small area over which the stresses act.

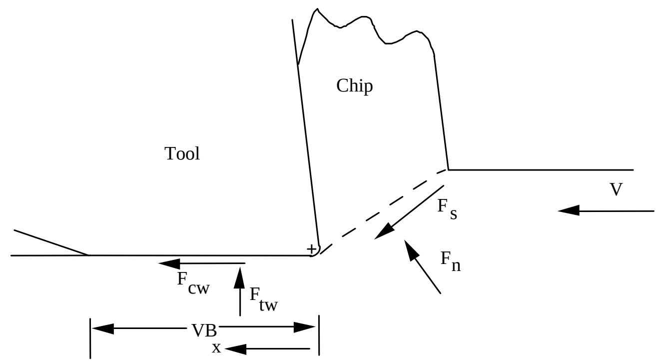

Figure 1 Cutting Tool with Flank Wear Land

The force components acting on a worn tool depend on the geometry of the cut and the stresses acting on the flank and along the shear plane. The shear plane forces are estimated (based on Oxley's model, [13]) as

$$F_s = k \cdot \frac{t_u}{\sin(\phi)} \cdot w$$
$$F_n = F_s \cdot \left(1 + \frac{\pi}{2} - 2 \cdot \phi - C_n\right) \qquad (2)$$

where k is the equivalent work material shear flow stress at the shear plane, t_u is the uncut chip thickness, w is the width of cut, and C_n is a constant (replaces the two constants C and n in Oxley's approach) representing the change in flow stress as material passes through the shear zone. Both k and C_n can be determined from orthogonal cutting experiments with a sharp tool, provided the uncut chip thickness is much larger than the edge radius so that ploughing can be neglected.

The wear forces depend on the normal and shear stresses acting on the flank according to Equation (1). The stresses along the wear land depend on the nature of tool-work. Both elastic contact, in which plastic flow occurs only at the tips of contacting asperities, and plastic contact, in which bulk plastic flow of the workpiece occurs below the flank, have been observed in published literature and are considered in this work.

Elastic Flank Contact

The variation of the normal stress along a wear land in elastic contact is based on the parabolic distribution of stresses from the theories of Johnson [11], Gladwell [14], and others [10, 9, 7] who have used it to describe normal stress on a tool flank. As a first approximation to be used for predicting wear forces, a fourth-order polynomial form is proposed, in which the stress at the tool edge, x = 0, is equal to the normal stress predicted for ploughing σ_0 based on the slip-line method described in [12] and the stress at the end of the flank, x = VB, is equal to zero

$$\sigma_W(x) = \sigma_0 \cdot \left(\frac{VB - x}{VB}\right)^4 \quad 0 < x < VB. \tag{3}$$

The latter condition matches results in [9] and reflects the effect of a slight inclination of the wear surface to the cutting velocity direction as observed by Usui [7] (see also the tilted punch discussion in Gladwell [14]). The flank shear stress under elastic conditions is proportional to normal stresses by μ, the coefficient of Coulomb friction.

Plastic Flow Below the Flank

For significant plastic flow of work material, a different approach is needed to estimate the flank stresses. The slip-line field proposed in [12] gives the stresses at the bottom of the tool tip, representing the beginning of the wear land (x = 0 in Figure 1). The results in [12] can be simplified by assuming that no significant bulge or prow is formed in the workpiece ahead of the advancing tool and that the friction between the work and any build-up at the tip is completely adhesive with the adhesive friction constant equal to 1.0. Under these conditions the normal and shear stresses (similar to those proposed by [15]) at the underside of the tool tip are

$$\begin{aligned} \sigma_0 &= k \cdot \left(1 + \frac{\pi}{2}\right) \\ \tau_0 &= k \end{aligned} \quad . \tag{4}$$

k is the equivalent shear flow stress described above. The shear stress on the flank depends on an adhesive friction factor m according to

$$\tau_W = m \cdot \tau_0 = m \cdot k. \tag{5}$$

Constant normal stress on the flank is given by

$$\sigma_W = \sigma_0 = k \cdot \left(1 + \frac{\pi}{2}\right). \tag{5}$$

Combined Elastic-Plastic Contact

A new approach is proposed in which both types of contact are considered. The model is characterized by a level of flank wear VB* at which plastic flow is initiated over part of the flank interface. The model assumes a transition develops between the plastic flow at the cutting edge (where stresses and temperatures are high) and the elastic contact on the flank. The plastic-to-elastic transition point is expected to shift away from the cutting edge as the wear land grows. This is based on results in [2, 3], in which a change was observed in the behavior of normal flank stresses after a certain VB had been reached, suggesting that initial elastic contact led to eventual development of plastic flow. Proposed stresses (Figure 2) for VB > VB* are

$$\sigma_W(x) = \begin{cases} \sigma_0 & 0 < x < VB - VB^* \\ \sigma_0 \cdot \left(\frac{VB - x}{VB}\right)^4 & VB - VB^* < x \end{cases} \tag{7}$$

and

$$\tau_W(x) = \begin{cases} \tau_0 & 0 < x < VB - VB^* \cdot \sqrt[4]{\frac{\tau_0}{\sigma_0}} \\ \mu \cdot \sigma_W(x) & VB - VB^* \cdot \sqrt[4]{\frac{\tau_0}{\sigma_0}} < x \end{cases} . \tag{8}$$

For VB < VB*, the normal stresses are as in Equation (3), with the shear stress given by

$$\tau_W(x) = \begin{cases} \tau_0 & 0 < x < VB \cdot \left(1 - \sqrt[4]{\frac{\tau_0}{\sigma_0}}\right) \\ \mu \cdot \sigma_W(x) & VB \cdot \left(1 - \sqrt[4]{\frac{\tau_0}{\sigma_0}}\right) < x < VB \end{cases} . \tag{9}$$

In (9) a transition region is needed near the tool edge (x = 0). In it, $\tau_W(x)$ is constant until $\sigma_W(x)$ decreases to the value τ_0/μ ($\mu \approx 1.0$ in the figure).

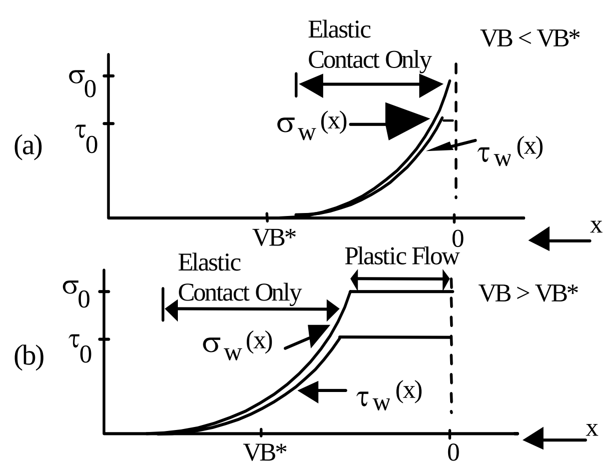

Figure 2 Normal and Shear Stresses on Wear Land for a) VB < VB* , b) VB > VB*

EXPERIMENTATION

A set of experiments was performed to isolate the effects of flank wear on cutting forces. The overall methodology was to first prepare cutting tools by naturally wearing down the flank during normal turning of an abrasive cast iron. Then, after measuring the wear land, a series of orthogonal tube-turning tests was run on 6061-T6 aluminum, during which the cutting forces were observed. The preparatory tests were run at a very low feedrate (approximately .02 mmpr), ensuring that any wear crater on the rake face was small and confined to the region just above the edge. The subsequent wear tests on aluminum were run at a constant feed of 0.2 mmpr. By keeping feed and speed (300 m/min) constant, it was assured that the strain and strain-rate in the shear zone was approximately constant across tests resulting in a constant shear yield stress - assuming negligible temperature effects from the wear land (see [16]).

Two tool types were used in the wear tests - Kennametal KC730 PVD coated carbide and K1 uncoated carbide inserts. Both types have edge radii typically less than .02 mm. Four edges were prepared from each insert type with VB levels up to a maximum of 0.38 and 0.49 mm, respectively. Additionally, repeated tests for each type with no wear land were run to "calibrate" a sharp tool model by estimating the material constants k and C_n needed in Equation (2). The same tool holder as used for all tests, giving a rake angle of 0^o and a clearance angle of 11^o. Due to set-up constraints the nominal width of cut varied by as much as 20% during the tests. Comparisons in the next section are therefore made for Forces per unit width, effectively eliminating the variable from the study.

The measurements taken during each wear test included the use of a dynamometer to record cutting and thrust forces. Chip dimensions were measured to determine the shear angle. In addition, a microscope with a position measuring device was used to measure the wear land on the inserts after the flank preparation tests. For each insert, four measurements of the width were collected at equally-spaced intervals along the flank.

OBSERVATIONS AND TEST RESULTS

The no-wear (VB = 0) and wear (VB > 0) cutting test results are summarized in Tables 1 and 2. The measured forces and shear angle ϕ from the tests with no wear were used to calibrate a sharp tool model and estimate the constants k and C_n, shown in Table 1, from the following equations,

$$k = \frac{F_c \cdot \cos(\phi) - F_t \cdot \sin(\phi)}{\overline{w} \cdot t_u} \cdot \sin(\phi) \quad . \qquad (10)$$
$$C_n = 1 + \frac{\pi}{2} - 2 \cdot \phi - \frac{F_t \cdot \cos(\phi) + F_c \cdot \sin(\phi)}{F_c \cdot \cos(\phi) - F_t \cdot \sin(\phi)}$$

In Equation (10) $\bar{w}$ is the average of uncut and cut chip widths (w and w_c, respectively) and F_c and F_t are the measured cutting and thrust forces, respectively (ploughing is neglected due to the large uncut chip thickness). Measured wear test conditions and results are

Table 1 Material Constants for Each Tool Type
(t_u = .2 mm, Speed = 300 m/min., Rake = 0 ^{o})

Insert Type	k (N/mm^2)	C_n
KC730	216.5	0.345
K1	210.9	0.418

Table 2 Wear Test Results
(t_u = .2 mm, Speed = 300 m/min., Rake = 0 ^{o})

Edge #	VB, mm	Width of Cut, w - w_c, mm	Shear Angle, ϕ, deg.	Forces, F_c, N	F_t, N
1*	.205	2.604 – 3.021	22.89	518.4	351.6
2*	.273	2.286 – 2.703	22.04	461.4	313.2
3*	.355	2.604 – 2.993	22.98	548.7	452.1
4*	.375	2.286 – 2.736	23.21	539.2	544.8
5**	.180	2.460 – 2.946	21.95	477.8	326.3
6**	.223	2.699 – 3.203	21.90	538.7	369.1
7**	.430	2.460 – 2.929	22.71	558.2	455.7
8**	.485	2.699 – 3.164	22.18	654.7	708.2

* - KC730 inset ** - K1 insert

summarized in Table 2, which gives the shear angle, uncut and cut chip widths, and forces for each test. Inspection of the inserts after the wear tests revealed contact over the entire width of the wear land. The shear angles in Table 2 are close to the angles measured from the unworn tool tests, 22.45 ^{o} for KC730 and 21.45 ^{o} for K1.

MODEL EVALUATION AND DISCUSSION

An evaluation of the proposed model is based on a comparison of predicted and observed values of total forces. Under the proposed model, no wear tests need be run before predictions can be made, although an estimate is needed of the point at which plastic flow is initiated below the flank.

The proposed model assumes that the shearing forces are unaffected by the presence of a wear land. The shear angles found during worn and unworn insert tests support the assumption by indicating that wear appears to have no effect on the shear angle, and hence shear strain in the shear zone. Because material constants k and C_n differ slightly across the tool types (Table 1), shearing forces are estimated using the constants appropriate for each. The values of k and C_n from Table 1 and the shear angles and chip thicknesses from Table 2 are directly inserted into Equation (2) to obtain shearing forces per unit width for each wear test.

Wear Land Force Estimates

The procedure for estimating the forces on the wear land depends on the wear land width to be used for the prediction. For VB < VB* (elastic contact only), Equations (3), (4), and (9) are used to define $\sigma_w(x)$ and

$\tau_w(x)$ along the wear land. For VB > VB* (combined elastic contact and plastic flow), Equations (4), (7), and (8) will generate the stress distributions. Equation (1) is used in either case for the total forces F_{tw} and F_{cw} due to wear. Some simple assumptions are needed to estimate the wear land forces. The coefficient of friction μ in Equations (8) and (9) is expected to be much higher than normally found between sliding metals due to the high temperatures and stresses on the flank. A value of approximately 1.0 has been observed by a several researchers [5, 7, 9, 16] and reflects the chemically clean contact between tool and work. The same value is assumed here for all predictions. When plastic flow occurs on the flank, the factor m is needed in Equation (5) and the wear land transition level VB* is required in (7) and (8). The friction factor m is assumed to be high and a value of 1.0 - corresponding to complete adhesion – is assumed due to the extreme conditions near the tool edge (see [11]). Finally, a value of VB* = 0.25 mm is assumed, based on the change of stress behavior seen in [2] for the same work material.

In summary, the following inputs are needed to predict forces as a function of VB:

- k, shear yield stress: from Equation (10) and measured conditions from sharp tool tests;
- μ, coefficient of Coulomb friction: likely high, 1.0 assumed here and elsewhere;
- m, adhesive friction: likely high, 1.0 assumed;
- VB*, wear land level at which plastic flow is initiated on flank: 0.25 assumed for 6061 alum.

Figures 3 and 4 show the total predicted and measured forces from the wear tests (normalized by width of cut). The measured forces show great similarity to those reported in [2]. Predictions exactly match measured forces for VB = 0 since measured forces at this condition were used to estimate k and C_n for the shearing force predictions. Therefore, the wear model is only tested for VB > 0. As seen in the figures, both cutting and thrust forces are predicted quite well by the model. Most measurements fall within 10% of predicted values, with a maximum error of less than 25% despite a 30–100% rise in forces. Uncertainty can be attributed to chip width variation, friction factor assumptions, and slip-line approximations.

Comparisons to Published Data

Sufficient process descriptions of wear tests for comparison are found in [1] and [3]. Data [3] for annealed 4140 steel, along with predictions, are given in Figure 5. In the figure, the dashed line shows the increase predicted for VB* = 0.25 mm. Although the cutting force appears to be somewhat underpredicted on average, the overall magnitudes and trends are captured well by the predicted lines – including the transition from elastic to plastic contact corresponding to the VB* = 0.25 mm point. Results [1] from tests on cold-drawn 1112 steel are

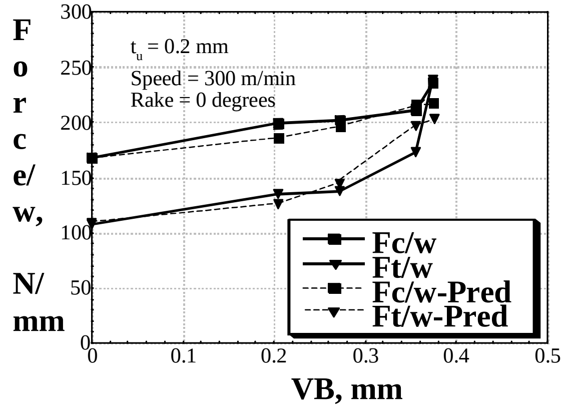

Figure 3 Predicted and Measured Wear Forces - KC730 Tools (VB* = 0.25 mm)

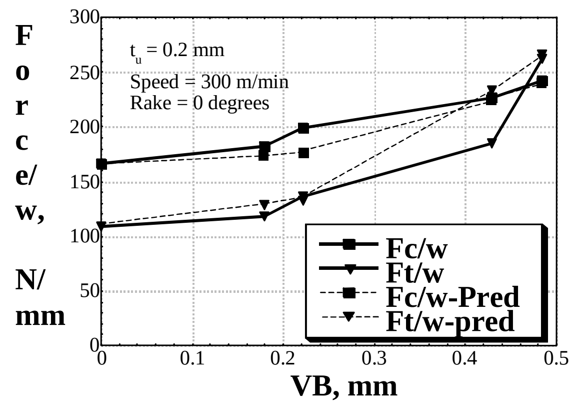

Figure 4 Predicted and Measured Wear Forces - K1 Tools (VB* = 0.25 mm)

shown in Figure 6. Again, dashed lines represent predictions based on VB* = .25 mm. General trends are predicted well, but the theory significantly overpredicts the slope of force increase in the plastic regime for cutting and thrust forces. This is almost certainly due to overestimating the shear flow stress along the flank. For comparison, identical predictions are shown (with dot-dash pattern) for a flank shear flow stress equal to one half the shear flow stress from the shearing zone. A nearly perfect fit is seen with this value. The unusually long

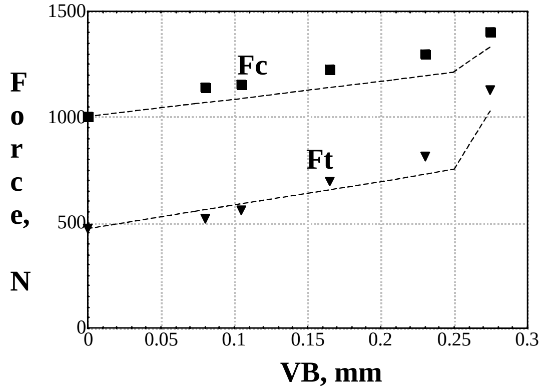

Figure 5 Data from [3] for 4140 Steel Showing Predicted Forces for VB* = 0.25 mm (dashed lines)

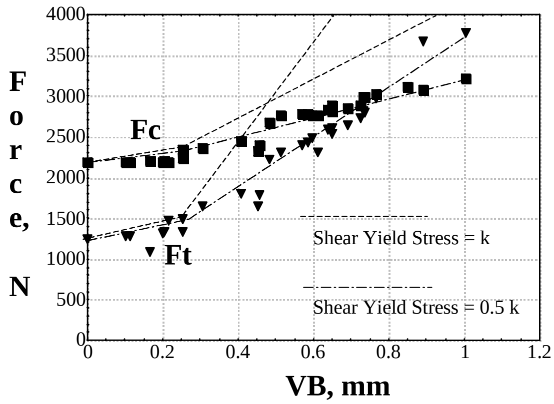

Figure 6 Data from [1] for 1112 Steel Showing Predicted Forces for VB* = 0.25 mm (dashed lines)

wear land (and, perhaps, the artificial grind-on procedure for the flank) appears to contribute to a lower yield stress on the flank. As mentioned in the Introduction, other researchers have suggested this difference.

Though the proposed model does a good job predicting wear forces and explaining observed trends without the benefit of extensive wear tests, several limitations exist. One is the selection of the critical wear level VB* at which plastic flow is initiated near the tool. The proposed value is only hypothetical, and further study is needed to relate it to the material and friction properties involved. Secondly, although elastic contact is suggested over much of the wear land, the material elastic constants (e.g., modulus of elasticity) do not factor into the approach. The forces over the elastic region are wholly dependent on the ploughing stresses σ_0 and τ_0 due to the simple, approximate relation in Equation (3). Finally, some sensitivity is seen with regards to the assumption of a shear yield stress on the flank equal to the shear yield stress computed from the shear zone.

SUMMARY AND CONCLUSIONS

A wear force model, based on a previously developed ploughing model, is proposed which accounts for elastic contact and plastic flow at the tool flank. A transition point between the two mechanisms is assumed and required as input to the model. The following conclusions are drawn:

- The wear model does a good job predicting the increase in forces due to a wearing tool under the conditions imposed in this study.
- Experimental results suggest a value of VB* equal to 0.25 mm, which also agrees qualitatively with published results. The value, however, may be sensitive to material or shear yield stress assumed for the flank.

Future experimentation will be directed towards estimating flank shear flow stress and verifying the model results across a broader range of materials with naturally worn wear lands.

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